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GENERATION OF HYDROGEN DURING THE FIRST
THREE HOURS OF THE THREE MILE ISLAND ACCIDENT

Randall K. Cole, Jr.

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Sandia Laboratories
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ABSTRACT

Estimates of the amount of hydrogen generated by oxidation of fuel cladding during the first three hours of the Three Mile Island accident are presented. These results have been obtained using a combination of available data, carefully framed assumptions, and simple analytic models, and are compared with results from an existing computer code. Hydraulic behavior has been described by a boildown process, with the top of the core uncovered about two hours after scram. The liquid level then fell in an exponential manner until, an hour later with only two feet of the core still covered, a reactor coolant pump was restarted. Core thermal behavior was found to be very dependent on cooling by the water vapor generated in the boildown process. Two limiting assumptions, of adiabatic fuel heating and of perfect thermal contact between the fuel and the vapor, resulted in a factor-of-two difference in the amount of hydrogen produced. A factor-of-two difference was also noted for a five minute difference in the time the fuel was uncovered. Our best estimate is that about 35% of the total core fuel cladding was oxidized producing 350 kg of hydrogen by three hours after scram. Essentially all of it was produced after the primary system had been isolated by closing the pressurizer Electromatic Relief Block Valve. The distribution of damage is much better described as oxidation through the complete thickness of 35% of the cladding than as partial oxidation of most of the cladding. Bounding estimates suggest that not less than 20% nor more than 60% of the cladding was oxidized.



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FOREWORD

This study was undertaken in response to an informal request from the Fuel Behavior Branch of the Nuclear Regulatory Commission Office of Research. The information sought was an estimate of the amount of hydrogen generated due to fuel cladding oxidation during the early phases of the Three Mile Island accident. It was agreed at the outset that a number of assumptions would be necessary due to the paucity of data and the desire for a timely reply. The study was executed in this spirit; the reader should approach this document in a similar frame of mind.

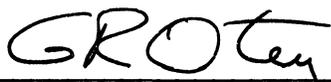
Oxidation of the fuel cladding and the attendant generation of hydrogen in a nuclear reactor core is a matter of great concern. Significant clad oxidation will only occur in an accident situation and this oxidation is a significant hazard in itself. The fuel cladding is the first of three barriers isolating fission products from the environment. The oxidation process can rapidly breach this barrier. Additionally, two moles of hydrogen are generated for every mole of zirconium cladding oxidized. Within the primary coolant system hydrogen is a noncondensable gas and for that reason, can seriously interfere with the reactor thermal/hydraulic behavior. A hydrogen explosion in the primary system is thought to be unlikely because of the low availability of oxygen. However, if released to the containment building (and this may be a necessary or unavoidable action during

certain accidents) hydrogen constitutes a serious threat in that an explosion could breach containment or disable critical components.

From this brief discussion it is easy to see why the subject of fuel cladding oxidation and the associated release of hydrogen, received so much attention during the Three Mile Island accident. The fact that the amount of hydrogen generated could only be inferred in a most indirect manner strongly indicates the need for better instrumentation. Additionally, it is very important to obtain a better understanding of:

1. Hydrogen chemistry and transport within the primary coolant system;
2. The limits of detonation and deflagration for H_2-O_2 -steam mixtures at various pressures;
3. The potential consequences of explosions;
4. The blast levels to which critical components should be qualified; and
5. Methods for mitigating the consequences of hydrogen release should fuel cladding oxidation occur.

Developing an attack on these issues is not the purpose of this paper. However, the reader is encouraged to consider, throughout the sequences described herein, how progress in the areas cited above might have been of use.



G. R. Otey, Manager
LWR Safety Department, 4440

GENERATION OF HYDROGEN DURING THE FIRST THREE HOURS OF THE THREE MILE ISLAND ACCIDENT

INTRODUCTION

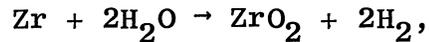
Any calculation of fuel cladding oxidation and the attendant hydrogen generation during an accident such as the one at Three Mile Island requires an understanding of both system hydraulics and core thermal response. Because of limited data and time constraints our basic approach has been to proceed from first principles, using simple analytic models and checking the predictions against such data as were available.

Our intention was not to develop a general calculational capability, but rather to understand the response of the system and the importance of various physical processes in the probable accident sequence. Therefore, models have been simplified sufficiently to allow a hand calculation using the computer only for evaluation of a few definite integrals. The accident appears to have been of a type for which the BOIL 1 code should also be applicable, and the results of a BOIL 1 calculation support our own analysis in many respects. However, because the basic physics is more deeply buried in the code calculation, and the results display some unphysical behavior not present in the analytic treatment, we feel that the code results should not be considered the more accurate.

We were forced to use estimates for a number of quantities when accurate values were not immediately available. It seems unlikely that any of these estimates is sufficiently in error to have a significant effect on the conclusions of this study.

BACKGROUND

During the accident at the Three Mile Island Nuclear Station, Unit 2 (TMI2), on 28 March 1979, it is believed that part of the core was uncovered for sufficient time to produce significant amounts of hydrogen by oxidation of zirconium in the fuel rod cladding. The reaction is



and produces 44.2 gm of hydrogen per kg zirconium oxidized. The heat of reaction is 6.51×10^6 J per kg Zr (2.80×10^3 BTU/lb_m).

The oxidation rate is assumed to follow the parabolic law

$$d\phi^2/dt = A \exp(-T^*/T)$$

where ϕ is the oxide thickness.

Several pairs of values for A and T* have been used, including the following:

Source	A	T*
RELAP 4, "BAKER-JUST"	.123 in ² /sec	41200 °R
RELAP 4, "CATHCART-PAWEL"	.0108 in ² /sec	36181 °R
TRAC "CATHCART"	.00349 in ² /sec	32512 °R
B&W [p6.2-9] ¹	.122 in ² /sec	40765 °R

The resulting correlations are compared in Figure 1.

The total mass of zirconium (based on 281.7 kg of clad, 98.04% zirconium, per metric ton of uranium, and 81.9 metric tons of uranium) is estimated² as 2.26×10^4 kg. Total oxidation would produce 999 kg

¹References in square brackets "[]" are to the TMI2 FSAR. In this case, we have corrected an obvious misprint.

²D. E. Bennett, Sandia Labs Org. 4414, private communication May 1979.

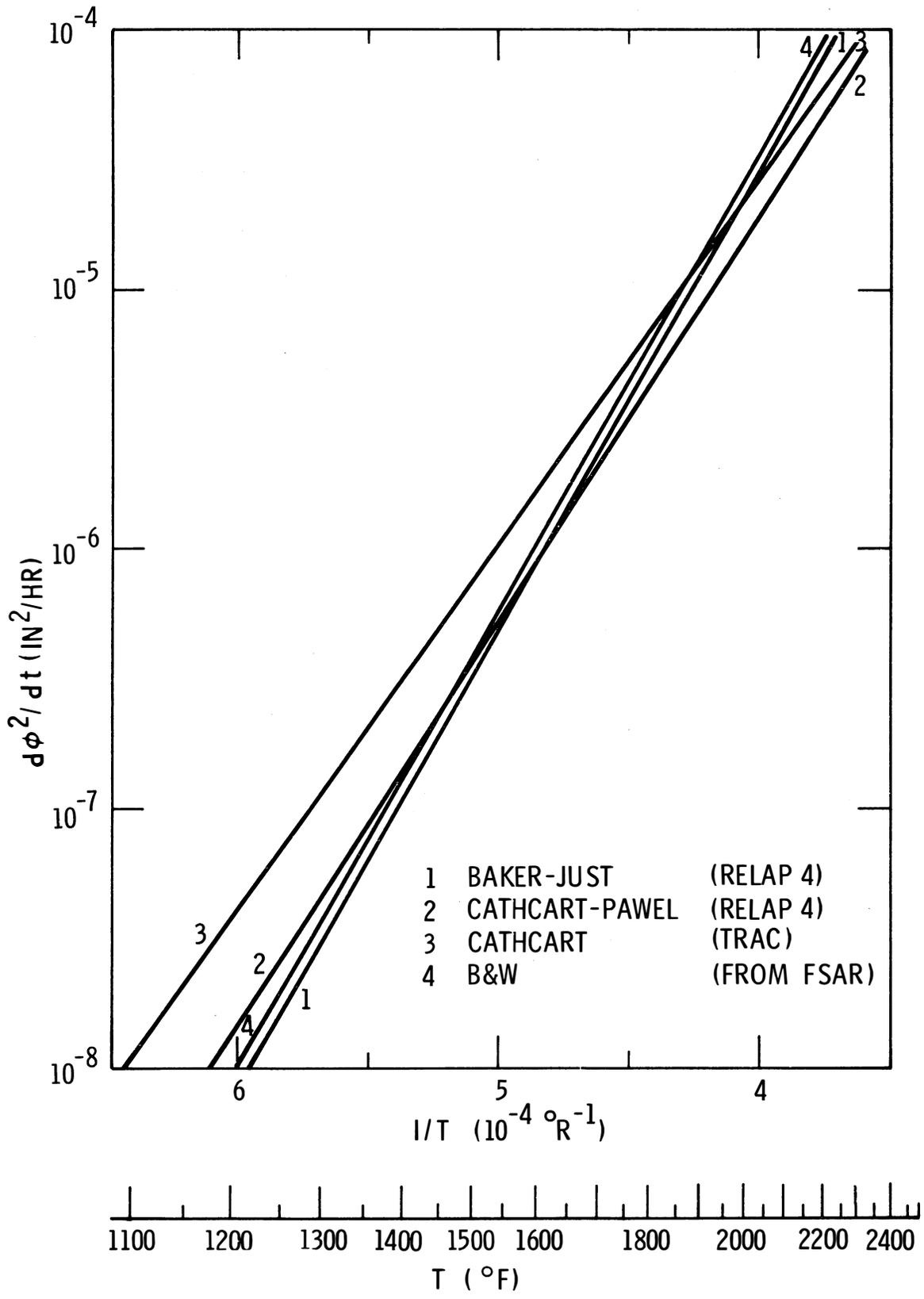


Figure 1. Zirconium Oxidation Rate

of hydrogen, representing 350 m^3 ($1.2 \times 10^4 \text{ ft}^3$) at 1000 psia and 600°F or $1.2 \times 10^4 \text{ m}^3$ ($4.2 \times 10^5 \text{ ft}^3$) at room temperature and one atmosphere.

HYDRAULICS

Rather little is known of the detailed hydraulics of the accident. Such data as are available to us are presented in Figs. 2 and 3 and Table 1.³ Fluid loss was through the Power Operated Relief Valve on the pressurizer. From the rated capacity of this valve, 112000 lb_m/hr at 2255 psig [Table 5.1-2], and the critical mass flux for saturated steam, Fig. 4 (from RELAP 4 Tables) we deduce that the effective area of this valve is .88 to .93 in^2 , depending on whether Moody or HEM critical flow is used. This is in good agreement with the value of .87 in^2 obtained from the equation⁴

$$KA (\text{in}^2) = \frac{W (\text{lb}_m/\text{hr})}{51.5 (1.1P (\text{psia}) + 14.7)} \cdot$$

If the valve throat diameter is $1\frac{5}{16}$ in., a possible value⁵, the latter value would correspond to a discharge coefficient of .64, which is not unreasonable.

It is clear that no detailed calculation of coolant loss is possible. Moody critical flow of saturated liquid at 1800 psia through a 0.87 in^2 effective area is about 60 lb_m/sec , less than the capacity of one HPI pump, 70 lb_m/sec (500 gpm [Table 6.2-1]). The HPI flow was apparently throttled to a much lower--and completely unknown--value. Therefore, we are forced to postulate a hydraulic history which seems consistent with the data.

³Staff Report on the Generic Assessment of Feedwater Transients in Pressurized Water Reactors Designed by the Babcock & Wilcox Company, NUREG-0560, May 1979.

⁴P. A. Schweitzer, Handbook of Valves, Industrial Press, 1972, p.148-149.

⁵D. Hoatson, Fuel Behavior Research Branch, RES, USNRC, private communication, May 1979.

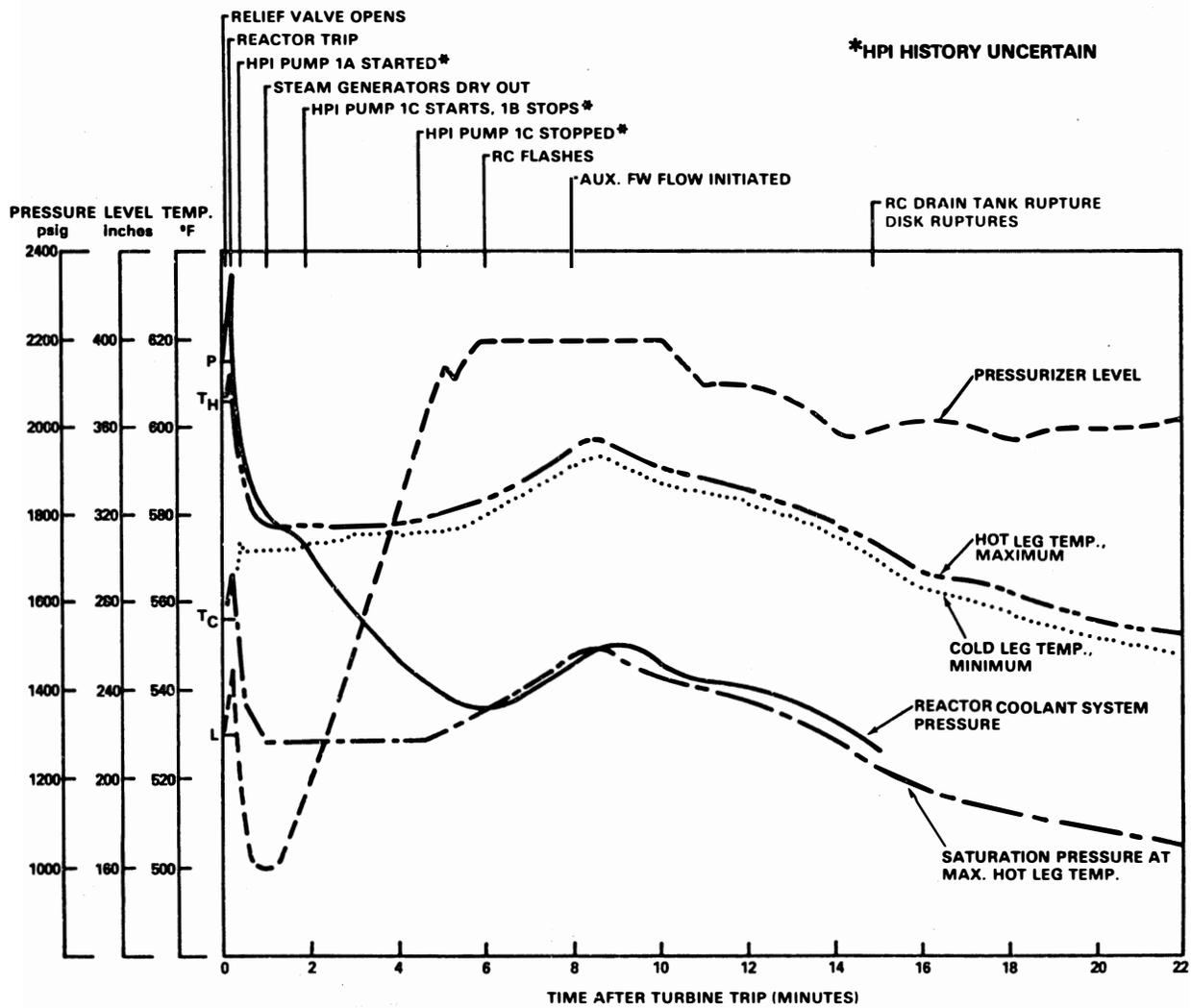


Figure 2. Reactor Coolant System Parameters in Minutes After Turbine Trip at Three Mile Island, Unit 2, March 28, 1979

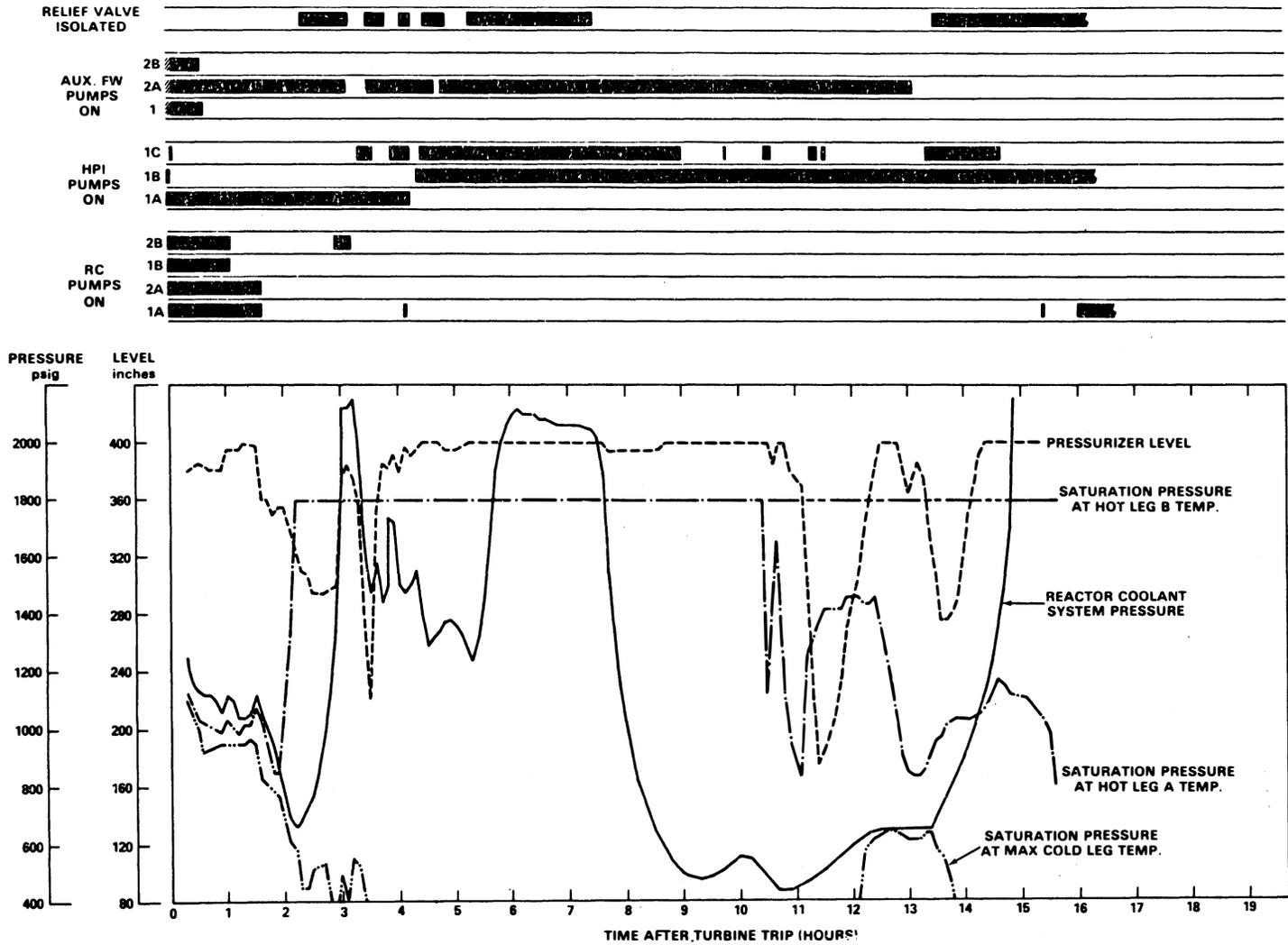


Figure 3. Reactor Coolant System Parameters in Hours After Turbine Trip at Three Mile Island, Unit 2, March 28, 1979

TABLE 1

Some Significant Times in TMI2 Accident Sequence*

<u>Time</u>	<u>Event</u>
2.1 min	High Pressure Injection Pump C (MU-P-1C) started automatically
4.6	The operator throttled the High Pressure Injection Isolation Valves (MU-V16's)
15	The Reactor Coolant Drain Tank rupture diaphragm (WDL-U26) failed
73	Reactor Coolant Pump 2B(RC-P-2B) was stopped
74	Reactor Coolant Pump 1B(RC-P-1B) was stopped
100	Reactor Coolant Pump 2A(RC-P-2A) was stopped
101	Reactor Coolant Pump 1A(RC-P-1A) was stopped
114 (Approx.)	Reactor Coolant System Loop A hot leg temperature began increasing
144 (Approx.)	The Electromatic Relief Block Valve (RC-V2) was shut
174	Operator started Reactor Coolant Pump 2B(RC-P-2B)
180 (Approx.)	Reactor Coolant System pressure increased to 2130 psig

*Extracted from a more detailed sequence in Reference 3.

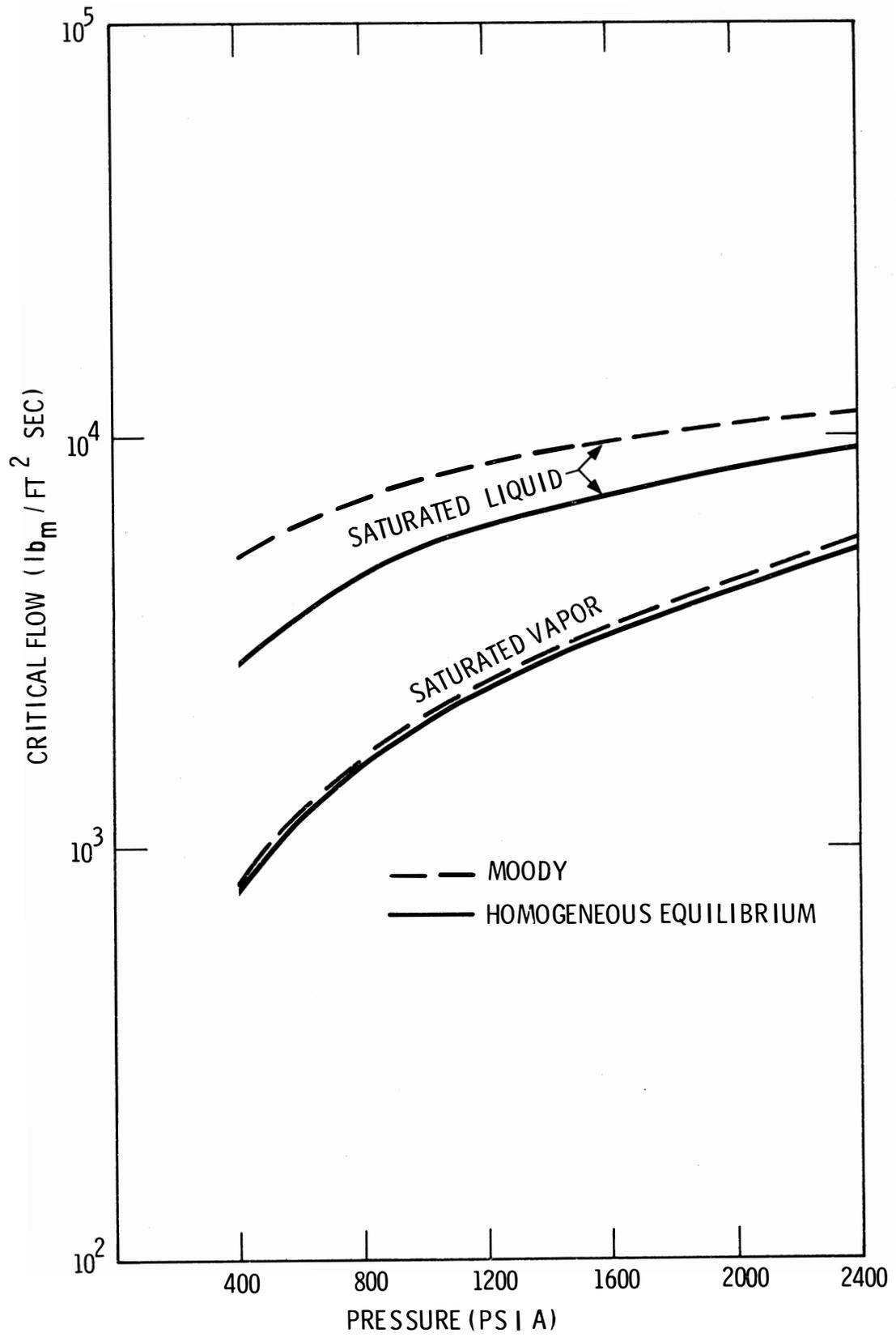


Figure 4. Critical Mass Fluxes

We assume that at about the time the last reactor coolant pump was shut off (101 minutes) there was no liquid above the high point in the cold legs at the pump outlet (see Fig. 5 [Fig. 5.1-5]) except in the pressurizer. From Figures 5 and 6 [Fig. 5.1-2] we estimate that this would require the loss of approximately $\frac{1}{3}$ to $\frac{1}{2}$ of the initial 5.0×10^5 lb_m (11126 ft³, 45 lb_m/ft³), depending on pressurizer contents. The "pressurizer level" in Figs. 2 and 3 is in fact a differential pressure, from near the bottom to near the top of the pressurizer, converted to head. It is probably a fair indication of pressurizer contents--nearly full most of the time--but clearly does not represent a well-defined liquid level. Assuming an average pressure of 1100 psi, the mass loss through a 0.87 in² effective area would be 1 to 3×10^5 lb_m in 101 minutes (depending on exit quality), making the assumed loss reasonable if the exit quality was fairly low and HPIS was essentially off.

Under these assumptions, the next phase of the accident was a simple boiloff of some fraction of the water covering the core, with the evolved vapor escaping or recondensing in cooler parts of the system such as steam generator A and the pressurizer. There may have been some liquid forced back through the pumps until the level fell below the bottom of the outlet nozzles, allowing free flow of the vapor.

The TMI2 core was quite young (2.6×10^5 MWd burnup); therefore, decay heat production was somewhat less than for an equilibrium core. The results of an ORIGEN calculation² using the actual fuel history⁶ are presented in Fig. 7 for the first 18 hours following the reactor trip. They show that the 1% power level (referred to 92% of 2772MWt) was reached in about 2 hours.

⁶R. G. Muranka, OMPA, and J. L. Crooks, MIPC, private communication to D. E. Bennett, Sandia Laboratories, Organization 4414.

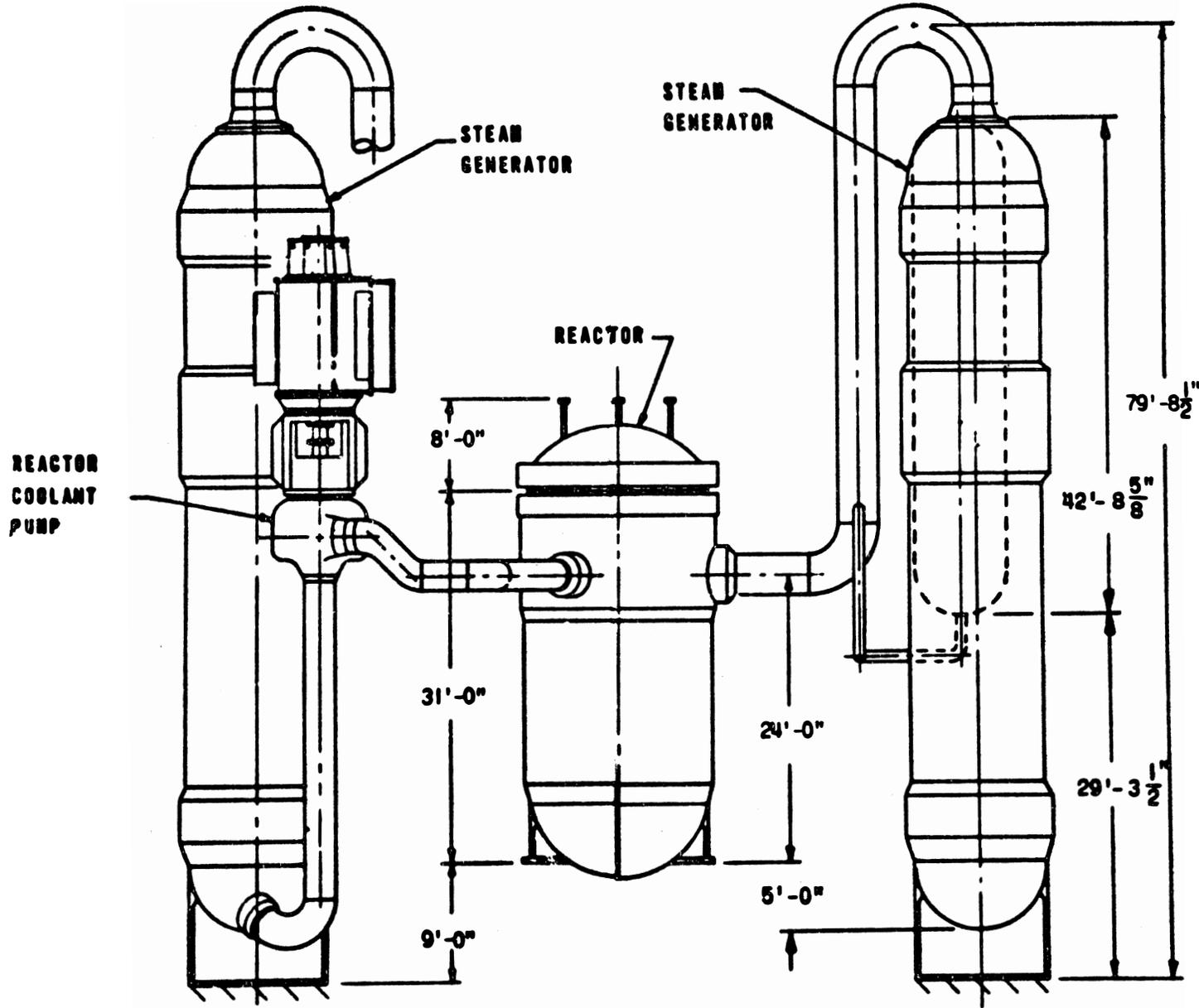


FIGURE 5.1-5

Figure 5. Reactor Coolant System Arrangement - Elevation, Three Mile Island Nuclear Station, Unit 2

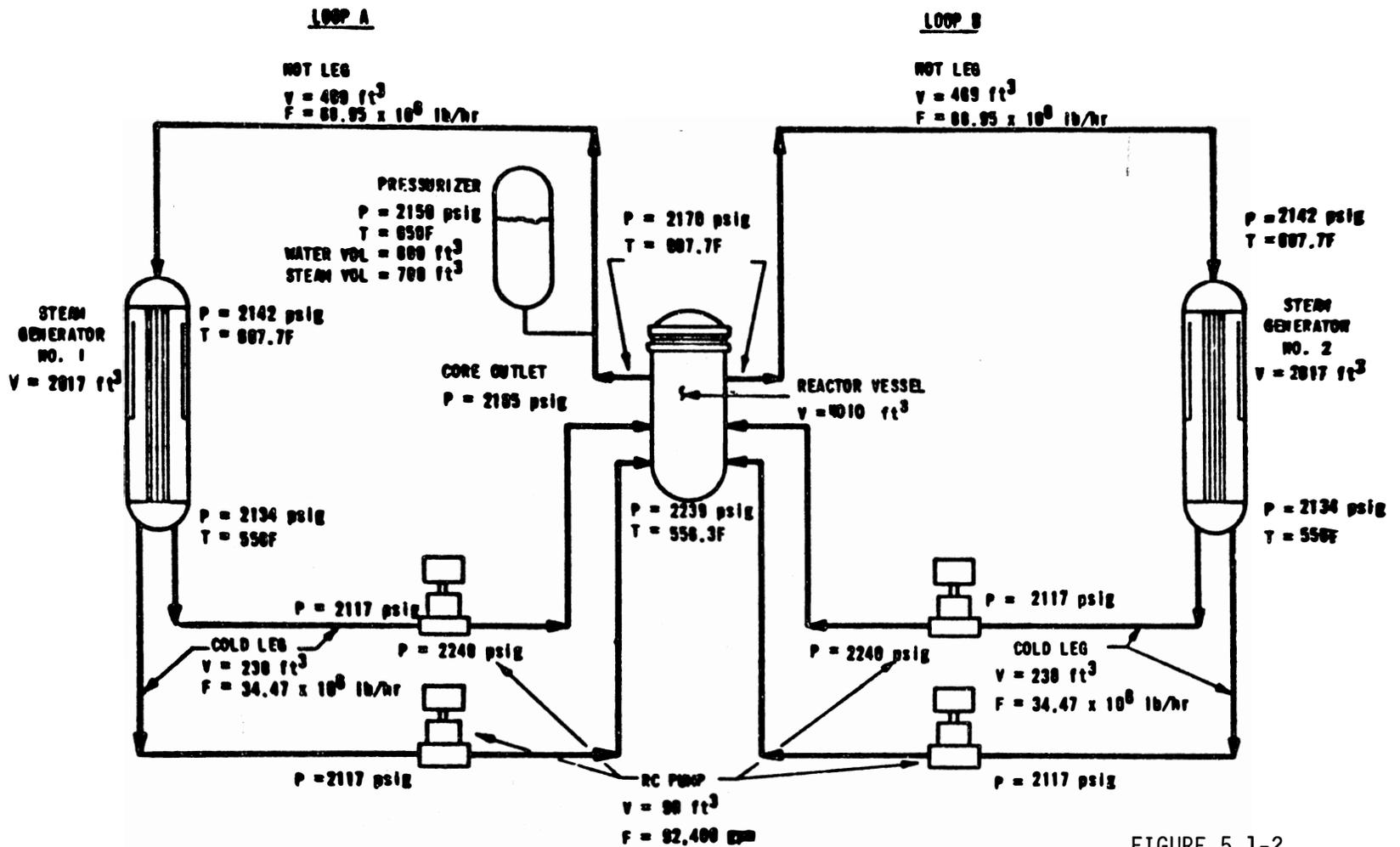


FIGURE 5.1-2

Figure 6. Reactor Coolant System Flow Diagram at Full Power Steady State Condition, Three Mile Island Nuclear Station, Unit 2

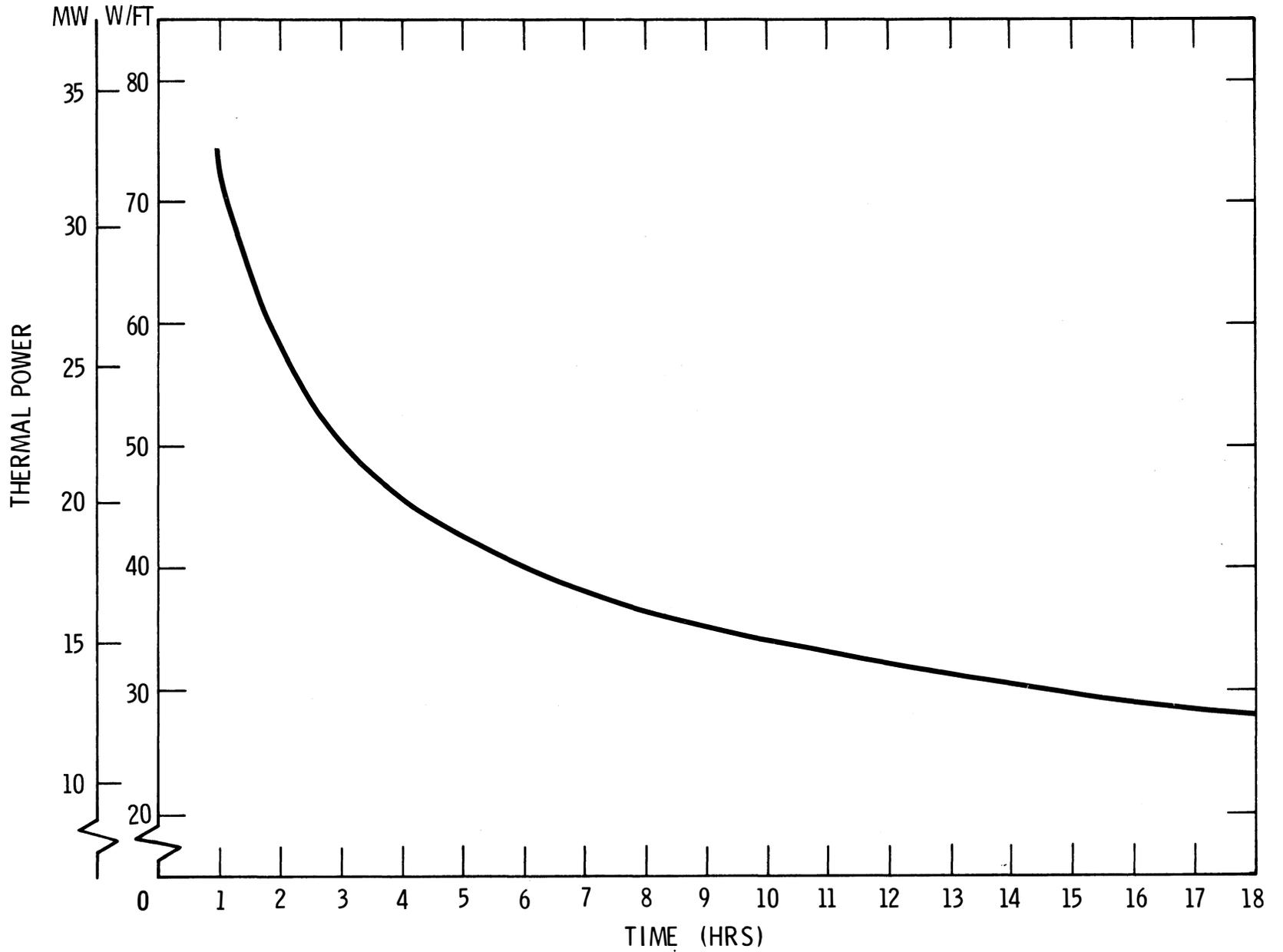


Figure 7. Decay Power, Three Mile Island, Unit 2,
Following Trip at 0400:45, March 28, 1979 (t + 45 sec)

We assume that all decay heat generated below the liquid level was carried off as latent heat of vaporization, with the temperature of the covered fuel remaining near the liquid temperature (saturation) temperature for the system pressure); that axial heat transfer by conduction and radiation are negligible; that the heat-storage capacity of the water and covered fuel can be neglected; and that there is no penetration of HPI water. This allows a simple estimate of the liquid level as a function of time through an energy balance. If axial peaking is also neglected, the necessary expressions are

$$\rho_l A_1 \frac{dz_l}{dt} = -Q/h_{fg} \quad z_l > H$$

$$\rho_l A_2 \frac{dz_l}{dt} = -\frac{z_l}{H} Q/h_{fg} \quad z_l < H$$

where

ρ_l = liquid density

z_l = height of liquid above bottom of core

H = core height

A_1 = effective cross-sectional area above top of core

A_2 = effective cross-sectional area between top and bottom of core

Q = decay heat generation rate

h_{fg} = latent heat of vaporization

If all time dependencies save that of z_l are neglected, we find

$$z_l = \begin{cases} H [1 - (t-t^*)/\tau_1] & t < t^* \\ H \exp [-(t-t^*)/\tau_2] & t > t^* \end{cases}$$

where

$$\tau_i = \rho_l A_i H h_{fg} / Q \quad i = 1, 2$$

and t^* is the time at which the top of the core is uncovered. We identify t^* as the time of appearance of superheated steam in the hot legs, 114 min.

The volume of the reactor pressure vessel with internals in place is given [Table 5.1-3 and Fig. 5.1-2] as 4010 ft^3 , while the subvolumes given in Table 4.4-4 total 3489.3 ft^3 . An attempt was made to reconcile these data using Figures 4.2-3 and 4.2-4. The "missing" volume appears to consist of 330 ft^3 between the core former plates and the core support barrel and 190 ft^3 between the outlet plenum assembly and the core support barrel. From these component volumes, we estimate that $A_1 \approx 150 \text{ ft}^2$ and $A_2 \approx 120 \text{ ft}^2$. A substantial fraction (20%) of A_2 is associated with the 330 ft^3 core former region. Figure 8 shows the calculated liquid level using $Q = 25 \text{ MW}$, $H = 12 \text{ ft}$, and material properties for an average pressure of 800 psia. We note that this figure shows roughly 4 ft of water over the core at 101 minutes, which is very near the elevation of the bottom of the pump outlet, in agreement with our original assumption.

We note from Figure 2 that system pressure began to rise at about 2.2 hr, reaching 1800 psi at 2.9 hrs, approximately the time when reactor coolant pump 2B was restarted and the preceding analysis becomes inapplicable. Toward the end of the repressurization the assumed constancy of material properties is violated. It is, however, only a 10% effect at 2.8 hrs when the pressure was 1200 psi, and is partially offset by the 10% decrease in Q from 2 to 3 hours.

The preceding analysis has neglected level swell. Assuming a core flow area of 49.2 ft^2 [Table 5.3-1] and a hydraulic diameter of .044 ft (deduced from a rod diameter of .430 in and pitch of .568 in [Table 4.2-1]) the Wilson bubble-rise model in RELAP 4 would predict

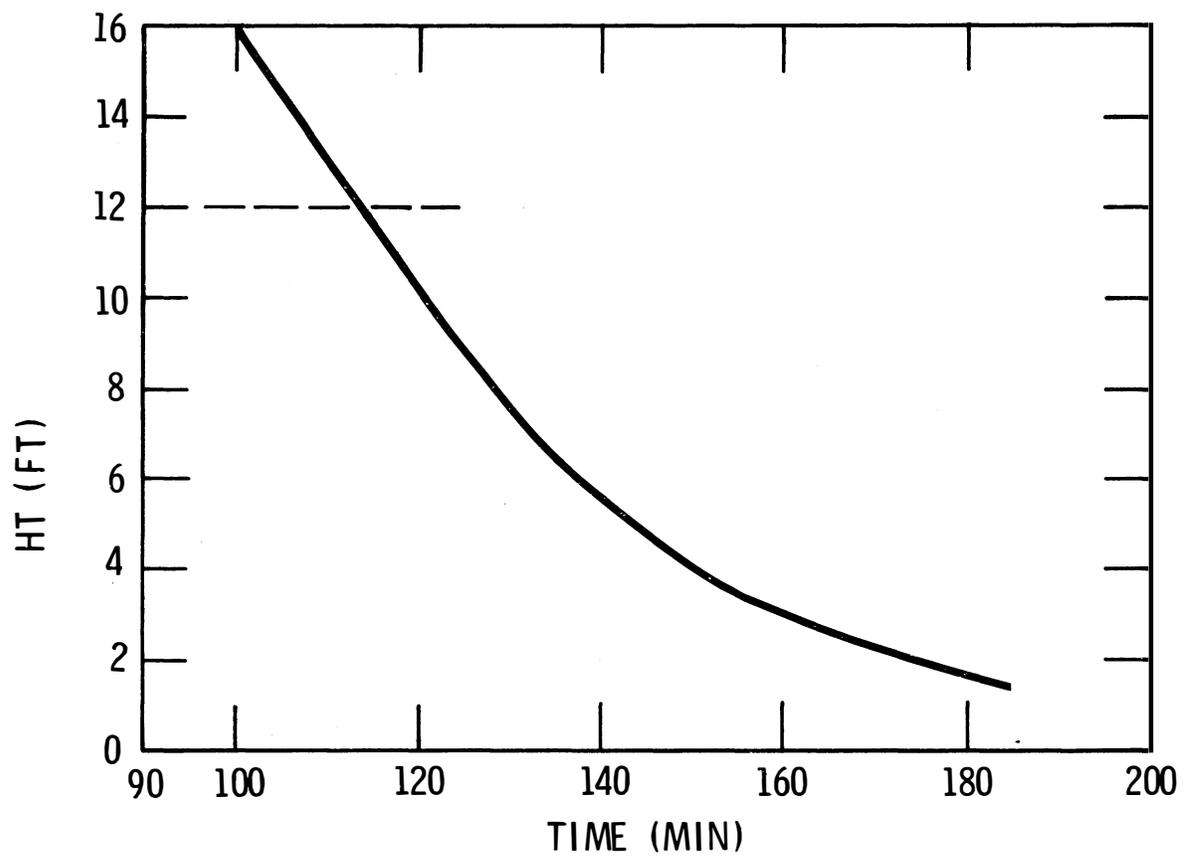


Figure 8. Estimated Level of Liquid Above Bottom of Core

a maximum void fraction at the surface of less than 2%, which is certainly negligible compared to other approximations made thus far.

Reactor coolant pump 2B was restarted at 174 minutes, and re-introduced an unknown quantity of fluid of unknown properties into the pressure vessel. In the absence of additional information, we feel that this ends the portion of the accident for which a reasonable calculation can be made. If the quantity of fluid and its temperature were known, one could presumably repeat the preceding analysis, starting at 193 minutes when the reactor coolant pump was stopped.

CORE THERMAL RESPONSE AND OXIDATION

Core burnup at the time of the accident was approximately 100 full power days. The distribution of fuel assembly powers in the core may be constructed from Fig. 9 [Fig. 4.3-9]. We have chosen to divide the assemblies into 10 equal-volume groups. The first group contains the hottest 10% of the assemblies and is assigned their average power, the second contains the next 10%, and so on to the coolest assemblies. The axial power distribution is constructed in Fig. 10, as an interpolation between 4 and 421 full power days [Figs. 4.3-17, 4.3-18]. These distributions and the uncover history of Fig. 8 are summarized in Fig. 11. The 10 assembly-power groups have been identified as "power regions" because, while not strictly true, it is convenient to think of them as corresponding to radial sections of the core. The central rectangle in Fig. 11 is then a pseudo cross-section of the core, on a volume basis.

The portion of the core above the liquid level would be expected to heat rapidly. The equation of overall energy balance, assuming that all axial heat transport is by convection of steam, is

$$\frac{\partial}{\partial t} (N m c_{pR} T_R + \rho_g A_2 c_{pg} T_g) + \frac{\partial}{\partial z} (W_g c_{pg} T_g) = Nq$$

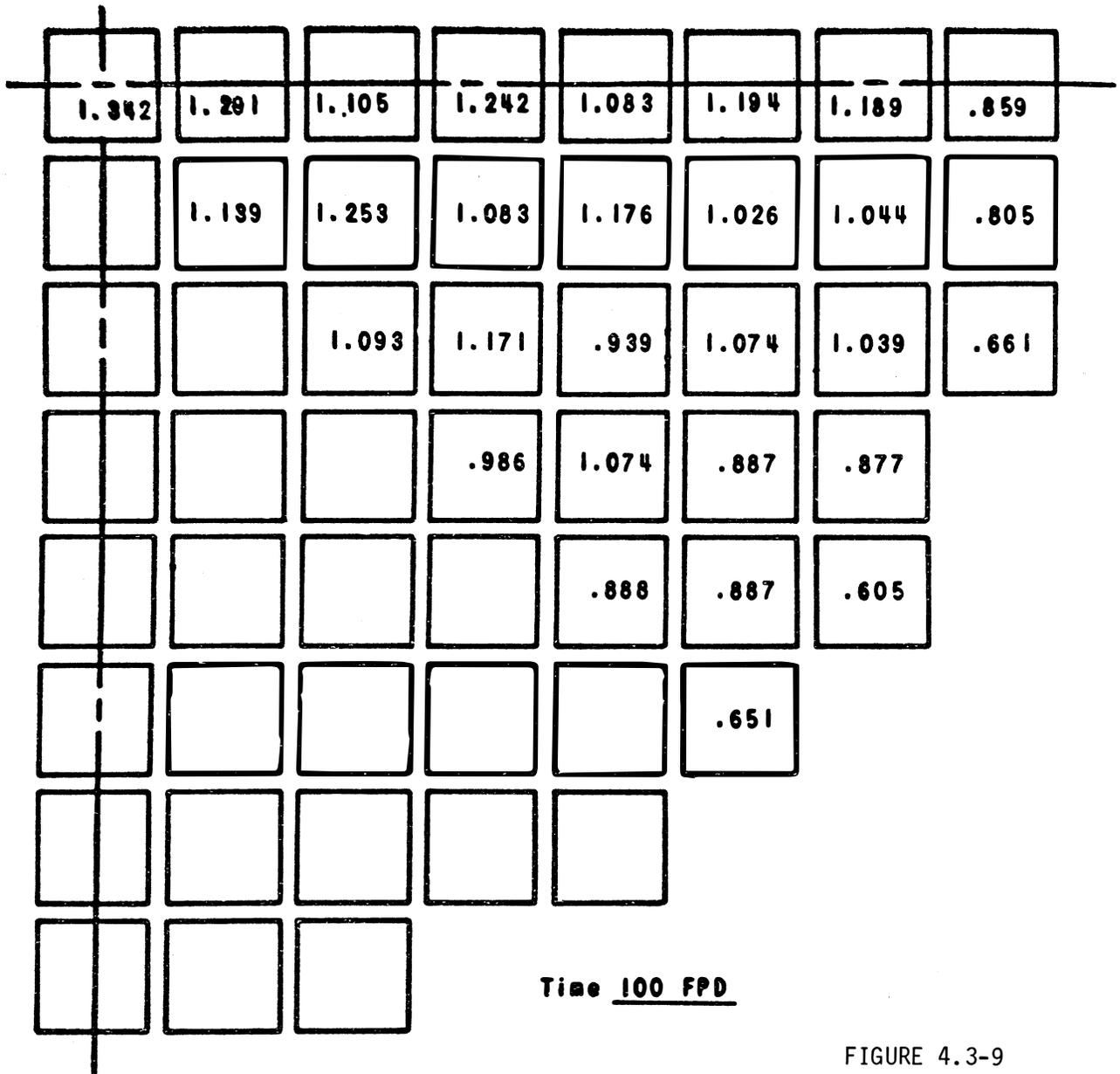


FIGURE 4.3-9

Figure 9. Assembly Relative Power Density,
100 FPD, Three Mile Island Nuclear
Station, Unit 2

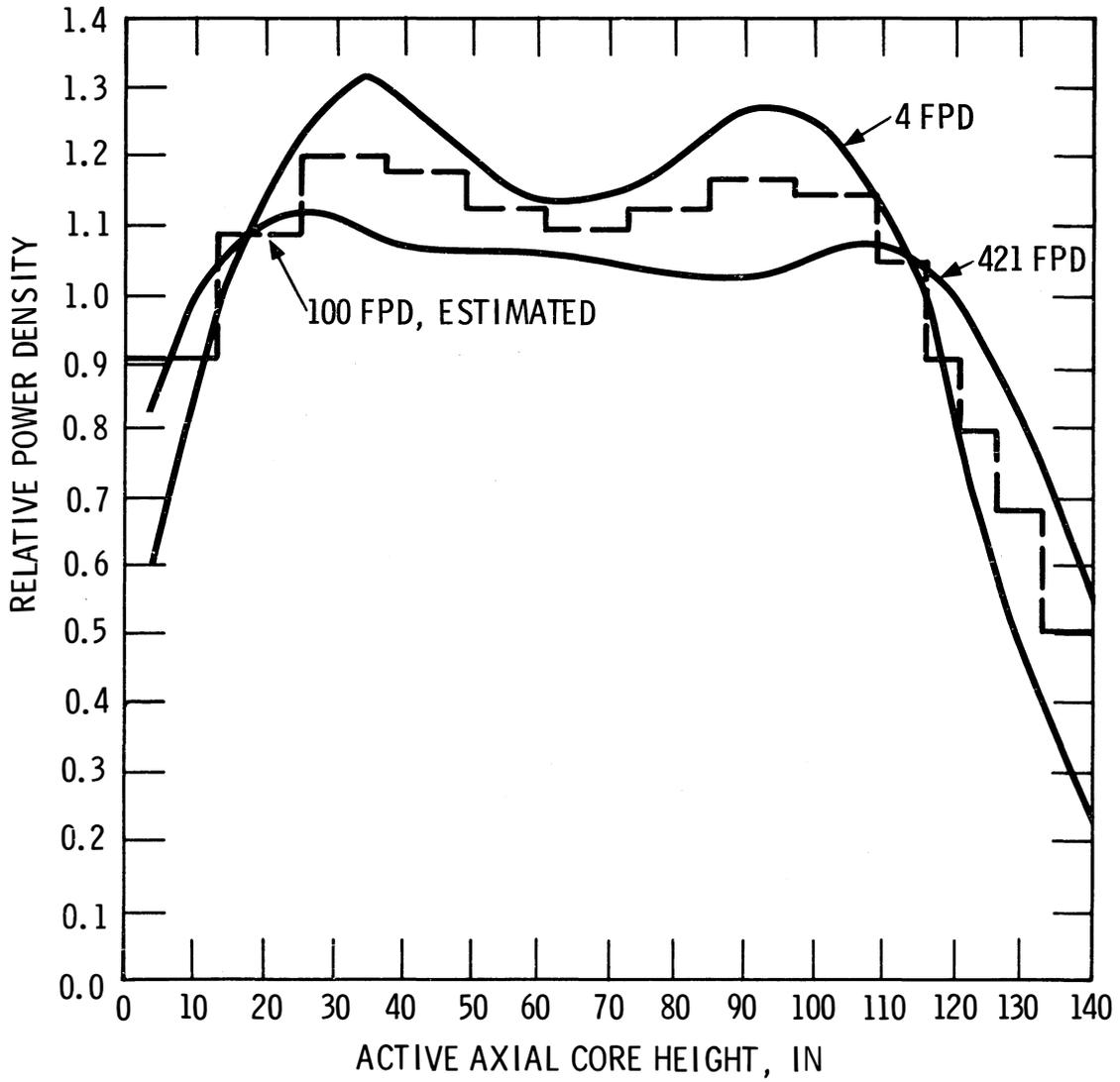


Figure 10. Axial Power Distribution

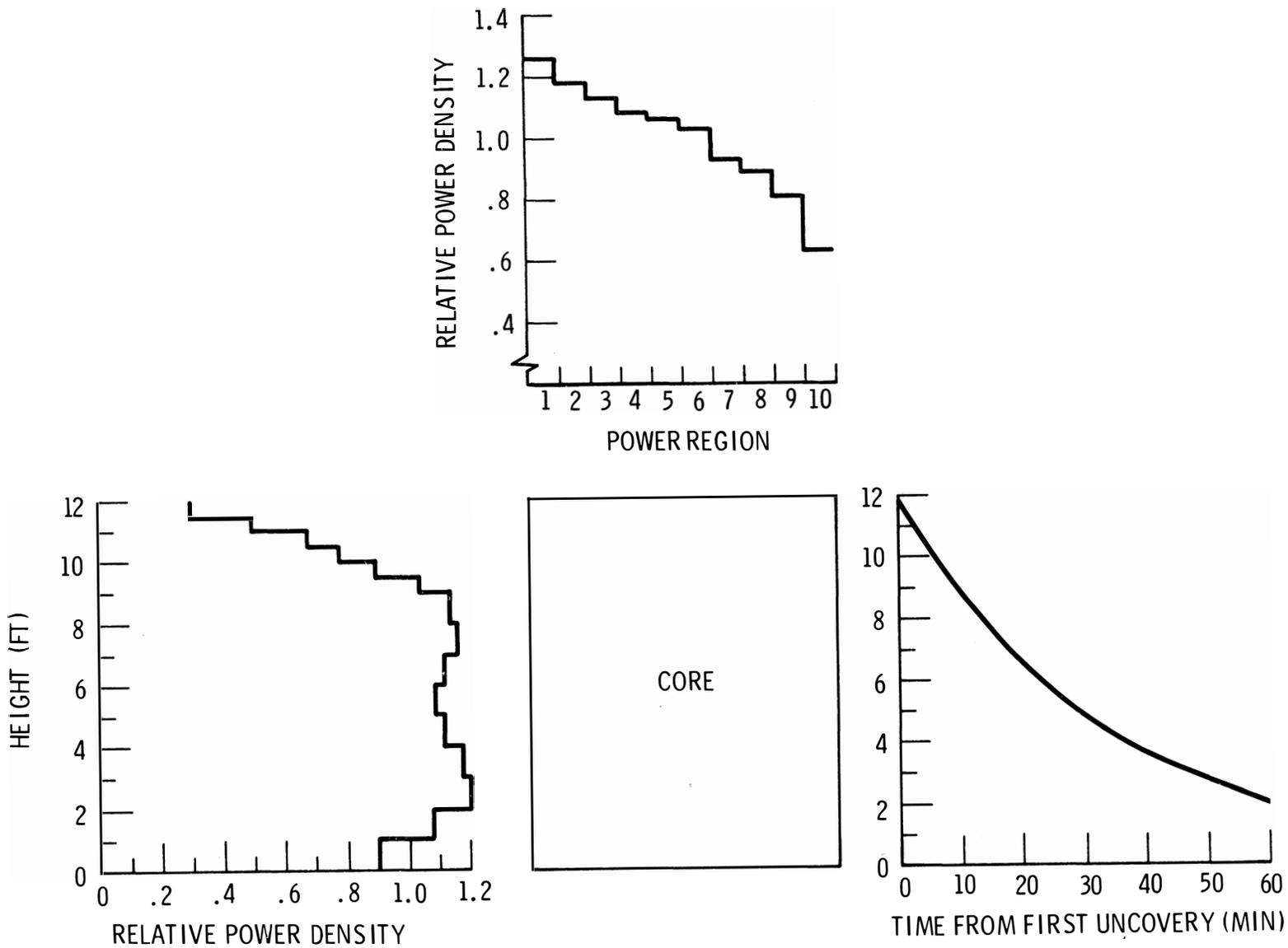


Figure 11. Power Density and Uncovery History for Core

Here

N = number of rods in the core

m = mass of a unit length of rod

ρ_g = density of vapor

c_{pR}, c_{pg} = heat capacities of rod and vapor

T_R, T_g = temperatures of rod and vapor

W_g = total vapor mass flow

q = heat generation rate per unit length of rod,
decay heat plus metal-water reaction heat

The second term in the time derivative is negligible (the rods have 100 times the heat capacity of the surrounding vapor) and will be neglected. The spatial derivative term describes the axial transport of energy by the vapor which is heated as it flows over the rods.

The rod and vapor temperatures are also related by the equation of energy balance for the rods alone,

$$\frac{\partial}{\partial t} (mc_{pR}T_R) = q - hA_R (T_R - T_g),$$

where

h = heat transfer coefficient, rod to vapor

A_R = surface area of a unit length of rod

The presence of liquid to a level $z_\ell(t)$ above the bottom of the core imposes a boundary condition that

$$T_R = T_g = T_O \text{ for } z \leq z_\ell(t)$$

where T_O is the liquid temperature.

For the simplified boiloff described perviously, the liquid level is given by

$$z_\ell(t) = H e^{-(t-t^*)/\tau_2}$$

with an axial vapor flow (independent of z) of

$$W_g = \frac{\partial}{\partial t} (\rho_l A_2 z_l) = \rho_l A_2 H \tau^{-1} e^{-t/\tau} ,$$

After some manipulation, the balance equations may be written as

$$\frac{\partial}{\partial t'} T_R + \lambda e^{-t'/\tau_2} \left(\frac{\partial}{\partial t'} + \frac{z}{\tau_2} \frac{\partial}{\partial z} \right) T_g = K$$

$$\frac{\partial}{\partial t'} T_R - h^*(T_R - T_g) = K$$

where

$t' = t - t^* - \tau_2 \ln(H/z)$, the time elevation z has been uncovered

$K = q/mc_{pR}$, the adiabatic heating rate

$\lambda = \rho_l A_2 c_{pg}/Nmc_{pR}$

$h^* = hA_R/mc_{pR}$.

and all material properties have been treated as constants. The dimensionless parameter λ is the ratio of the heat capacity of the vapor evolved while boiling the water level down by Δz to the heat capacity of the additional fuel thus exposed, and is a measure of the effectiveness of the vapor in carrying heat. The parameter h^* , an inverse time, describes the rate at which heat transfer drives T_R and T_g toward local equilibrium.

These coupled partial differential equations could be solved numerically. However, considerable insight may be obtained by examining two approximate analytic solutions. The first is adiabatic heatup for which $h^* = 0$ and

$$T_g = T_o = \text{constant}$$

$$T_R = T_o + \int_0^{t'} K dt''.$$

This is clearly an overestimate of the heating rate. The Dittus-Boelter correlation gives heat transfer coefficients from rod to vapor

of 5 BTU/hr ft² °F or greater. Decay heat generation in an average rod is about 200 BTU/ft hr and its surface area is .113 ft²/ft. Thus, convective heat loss would balance decay heat generation with a temperature difference, $T_R - T_g$, of only a few hundred degrees--the actual difference should be substantially less than this.

The fuel must be heated by perhaps 1500°F before a significant oxidation rate will occur. Therefore, it seems more reasonable to neglect $T_R - T_g$ compared to $T_R - T_o$ for most of the heatup, and to make the approximation that $T_g \approx T_R$. This is the opposite limit from adiabatic heatup, and corresponds to $h^* \rightarrow \infty$ with cooling limited not by heat transfer from rod to vapor but by the finite heat capacity of the available vapor. The resulting balance equation is

$$\left[(1+\lambda e^{-t'/\tau_2}) \frac{\partial}{\partial t'} + \lambda \frac{z}{\tau_2} e^{-t'/\tau_2} \frac{\partial}{\partial z} \right] T_R = K .$$

If we neglect reaction heat and the axial power shape, K is constant, and this equation has the analytic solution

$$T_R = T_g = T_o + K \left[t' - \tau_2 \ln \left(\frac{1+\lambda}{1+\lambda e^{-t'/\tau_2}} \right) \right]$$

Here the second term in the square brackets describes the effect of the vapor convection. Note that the temperature depends only on t' , the time that fuel has been uncovered, and not on its position in the core, so that T_R satisfies the ordinary differential equation

$$\frac{\partial}{\partial t'} T_R = K / (1+\lambda e^{-t'/\tau_2}) .$$

For $\lambda \rightarrow 0$ (small vapor heat capacity), the righthand side of this equation is K , giving adiabatic heatup, while for $\lambda \rightarrow \infty$ (large heat capacity) the righthand side is zero, giving no heatup. For intermediate values of λ , the vapor carries off part of the heat generated, and reduces the rod heating rate.

This analytic solution is valid only if the heat generation rate is constant. In truth, there is an axial variation because of the axial power shape and a temporal variation due to chemical reaction heat. We will ignore the axial shape and assume that the preceding equation applies to heating by decay heat. On the other hand, we will assume that convection is completely ineffective in removing reaction heat. This is because, as will be seen shortly, the reaction proceeds extremely rapidly once it is well started, and there is probably insufficient time to remove much of the heat generated. The resulting heating equation is

$$\frac{\partial}{\partial t} T_R = K_{\text{decay}} / (1 + \lambda e^{-t'/\tau_2}) + K_{\text{reaction}}$$

where the adiabatic heating rate K has been separated into

$$K_{\text{decay}} = q_{\text{decay}} / mc_{pR}$$

$$K_{\text{reaction}} = q_{\text{reaction}} / mc_{pR}$$

and q_{reaction} is the reaction heat from clad oxidation

$$q_{\text{reaction}} = \Delta H \rho_{\text{Zr}} \pi D \frac{d\phi}{dt}$$

where ρ_{Zr} is the density of zirconium in the clad, D is the average clad diameter, and other terms have been previously defined. We have termed this model "perfect contact" because of the assumed perfect thermal contact between fuel and vapor during most of the heatup. The core contains 36818 rods (177 assemblies of 208 active rods [Table 4.3-1]); at 25 Mw decay heat $q_{\text{decay}} = 56.6 \text{ watt/ft} = 193 \text{ BTU/hr ft}$. With mc_{pR} estimated as $.0444 \text{ BTU/ft } ^\circ\text{F}$ and an average $c_{pg} = 0.6 \text{ BTU/lb } ^\circ\text{F}$, we find $\lambda = 2.11$, $K_{\text{decay}} = 72.4 \text{ } ^\circ\text{F/min}$, and $\Delta H \rho_{\text{Zr}} \pi D = 1.008 \times 10^4 \text{ BTU/ft/in}$.

Using Cathcart-Pawel rate parameters, the heatup and oxidation equations have been numerically integrated from a starting temperature of 520°F (saturation at 800 psia) for both the adiabatic and perfect-contact models. The results for oxidation depth as a function of time for various rod powers are shown in Fig. 12. Because most of the thickness is oxidized very rapidly, driven by reaction heat, the time difference between oxidation of 5% and 100% of the clad thickness is quite small, assuming that adequate steam is available. We have therefore adopted a simple criterion: all clad uncovered for less than the time necessary to oxidize 10% of its thickness is considered untouched while all uncovered longer is considered completely oxidized.

The data summarized in Fig. 11 may be combined with the oxidation times of Fig. 12 to construct "maps" of the oxidized portion of the core. The procedure is simple: for each axial segment of each power region, we determine the time necessary to oxidize the clad, and compare with the time that that segment has been uncovered. The results for 55 minutes and 60 minutes after the top of the core is uncovered (at about 114 minutes) are shown in Figures 13 and 14 respectively. In these Figures the outlines enclose the effective region of complete oxidation--a more detailed calculation would show these hard outlines replaced by a relatively narrow band of partial oxidation. The first of these Figures corresponds to the state at 169 minutes, at which time the system pressure had reached 1300 psi and was rising extremely rapidly. The second is for 174 minutes, the approximate time that a reactor coolant pump was restarted, returning a large quantity of water to the core. We believe that the "perfect contact" results are more realistic. The slight optimism with regard to the effectiveness of removal of decay heat by forced convection should be offset by the neglect of any removal of reaction heat and by the neglect of limitation

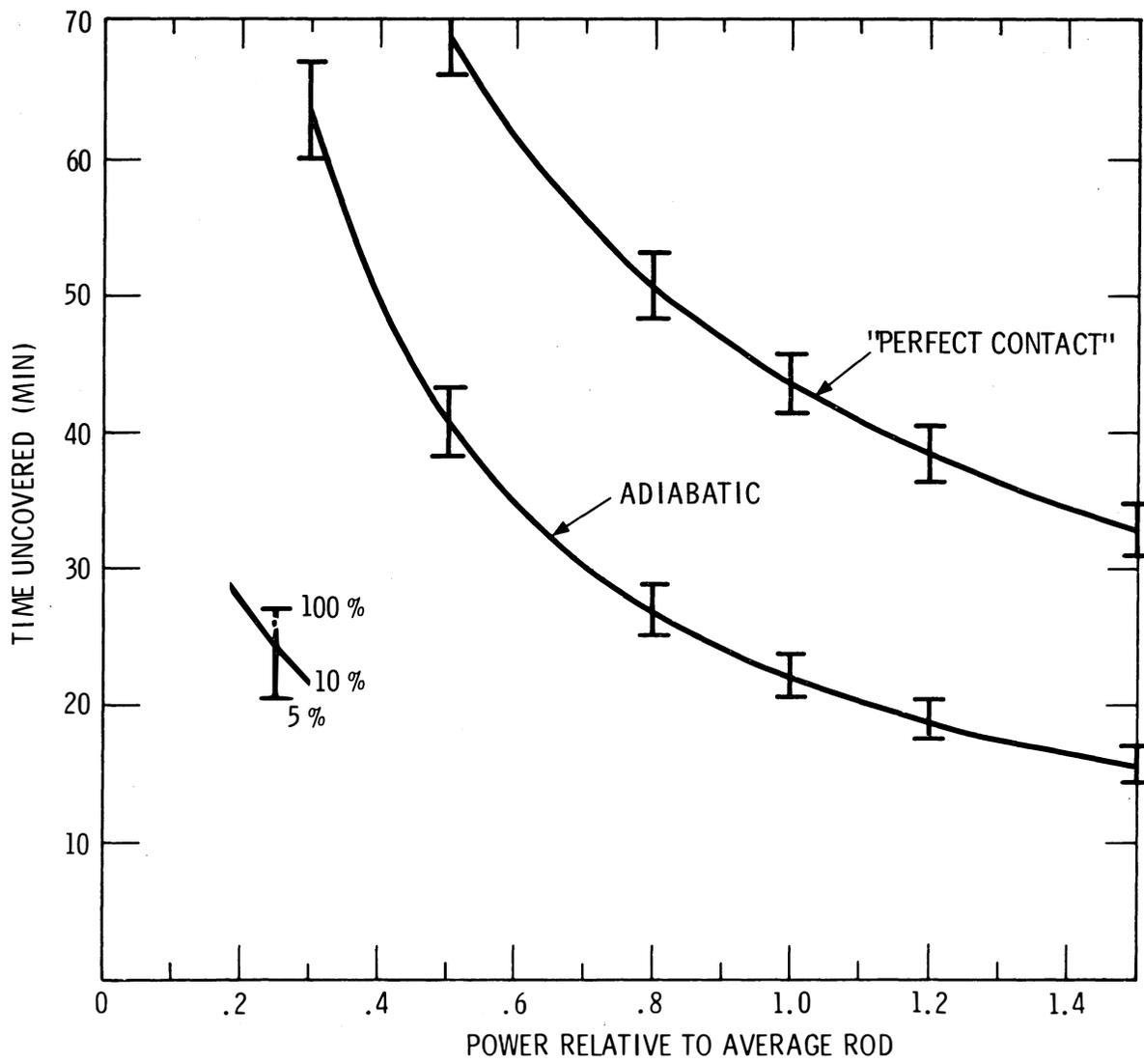


Figure 12. Time for Clad Oxidation versus Relative Power, Assuming Steam is Available

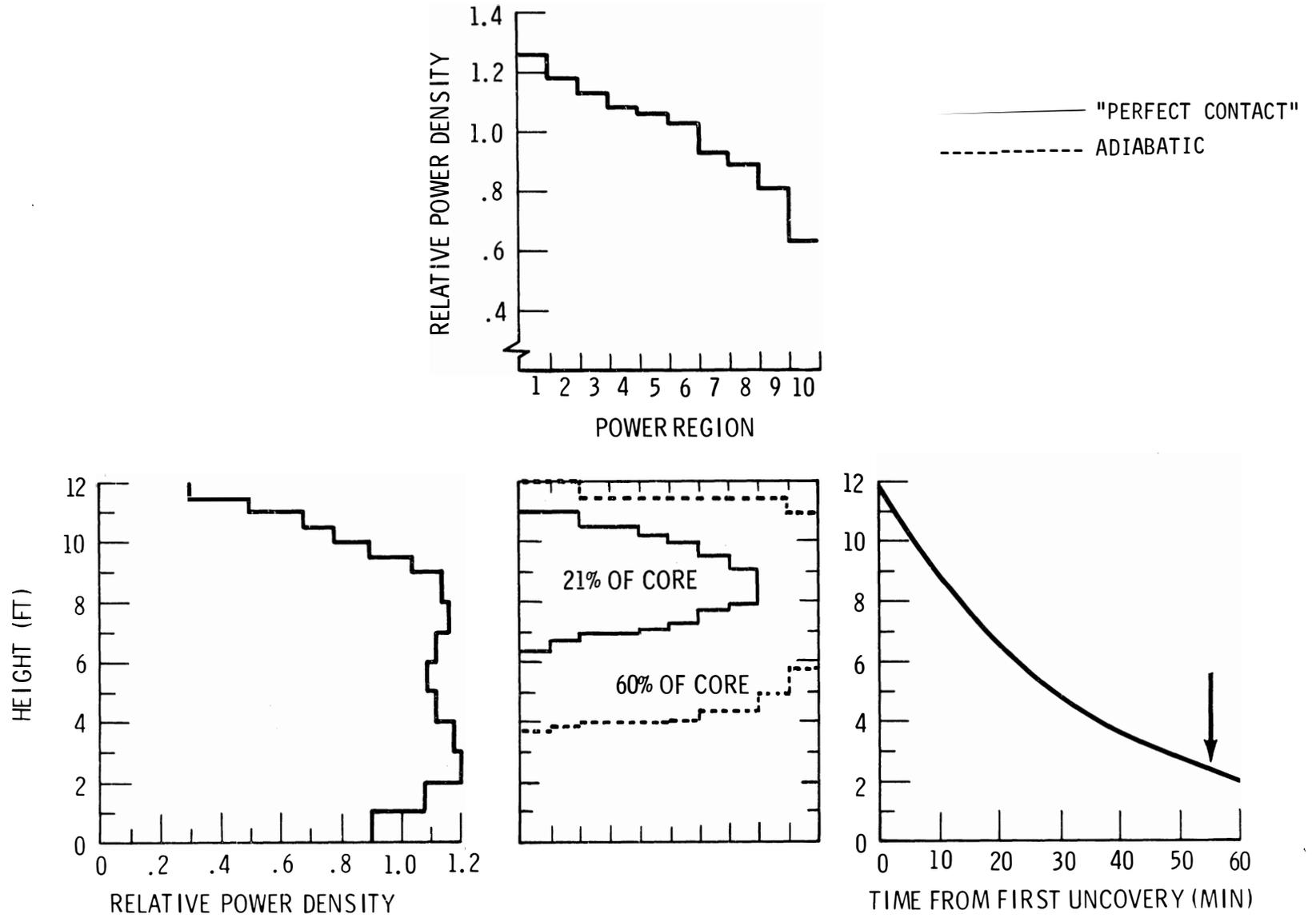


Figure 13. Boundary of Oxidized Region at 55 Minutes After First Uncovery

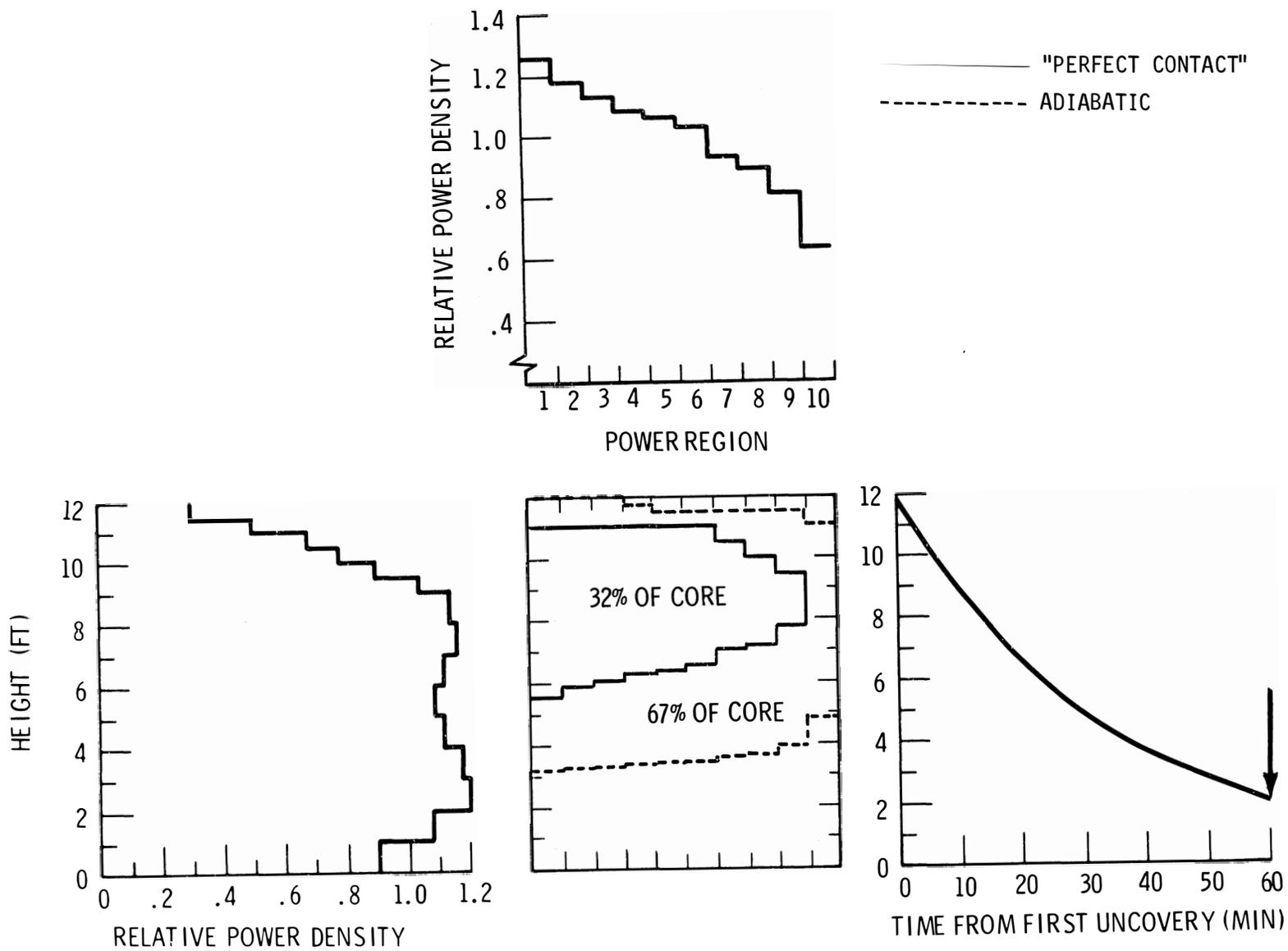


Figure 14. Boundary of Oxidized Region at 60 Minutes After First Uncovery

of the oxidation rate by steam availability (probably a small effect). However, given all the uncertainties in the problem, the resulting oxidations--21% at 169 minutes and 32% at 174 minutes should probably be considered as factor-of-two estimates.

COMPARISON WITH BOIL 1

We feel that the analysis presented shows that the basic physical processes involved in the accident are reasonably well understood, and the effects may be calculated using relatively simple models. The important processes, and several others, are modeled in the BOIL 1 code.⁷ We performed a calculation of the boildown portion of the TMI2 accident using input corresponding to our analytic calculation. A total of 48 axial regions was employed. Because our primary interest is in oxidation, we used the rod heat capacity of .0444 BTU/°F ft appropriate to temperatures less than roughly 2500°F and adjusted the fuel melt temperature to give the correct melt enthalpy.

The liquid level calculated by BOIL 1 is within a few tenths of a foot of that shown in Fig. 8, and the void fraction at the liquid surface is never greater than 4%. The oxidation history is shown in Fig. 15. We had modified the code to print an oxidation map analogous to Figs. 13 and 14. The results were rather peculiar: in some assemblies there was an alternation of 100% oxidation and less than 20% oxidation over several 3 inch axial segments. The reason for this is not understood. While no fuel melting was calculated in 60 minutes, fuel temperatures had risen to near melt in a large region of the core.

HYDROGEN PRODUCTION

There are many uncertainties in the problem, in particular the uncertain HPI history. Also, the extreme rate at which oxidation is

⁷BOIL 1, R. O. Wooten, Battelle Columbus Laboratories, 1976.

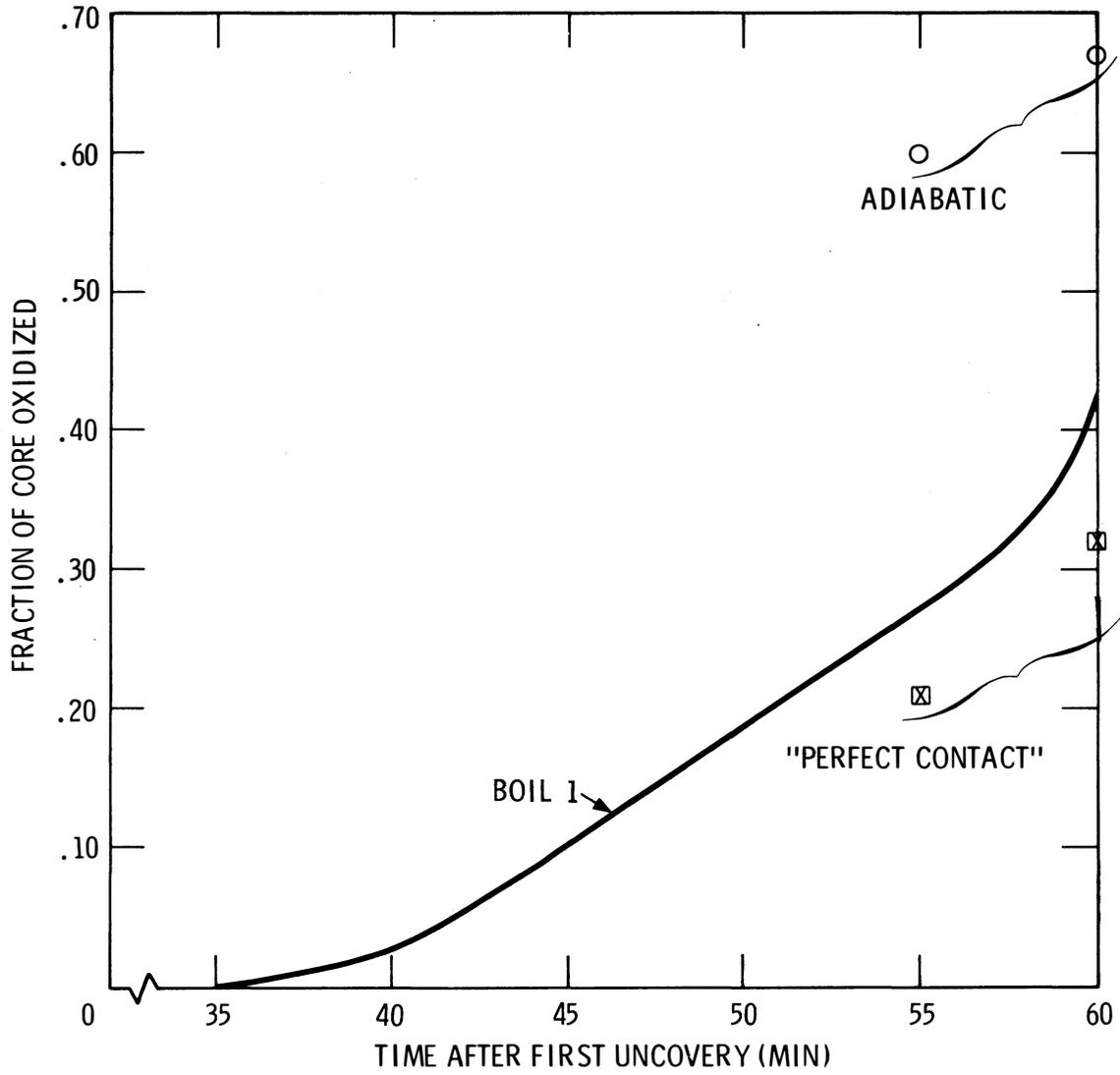


Figure 15. Clad Oxidation versus Time

calculated to occur near the end of the core uncovering makes timing crucial. Therefore, we feel that the preceding analysis should be viewed as the education necessary to make an educated guess of total oxidation and hydrogen production. Ours is 35% of the core oxidized and 350 kg of hydrogen produced. We feel that actual oxidation was probably more than 20% and less than 60%.

From Fig. 15, we see that almost no hydrogen was produced before 149 minutes (114 + 35). The blocking valve for the power operated relief valve was closed at 142 minutes. Therefore, essentially all hydrogen generated during this uncovering of the core was trapped in the primary coolant system until the blocking valve was reopened later in the accident sequence.

A large fraction of the production took place during the last few minutes before the reactor coolant pump was restarted, and may have been responsible for much of the rapid repressurization observed during this period. The system repressurized to 2100 psia. Assuming a temperature between 450°F (saturation at maximum cold leg temperature from Fig 3) and 640°F (saturation at system pressure after repressurization) the estimated 350 kg of hydrogen would have a volume of 2100 to 2500 ft³ (60 to 70 m³) at this pressure.

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